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An investigation has been conducted in the Langley 16-Foot Transonic Tunnel to determine the effects of varying six nozzle geometric parameters on the internal and aeropropulsive performance characteristics of single-expansion-ramp nozzles. This investigation was conducted at Mach numbers from 0.60 to 1.20, nozzle pressure ratios from 1.5 to 12, and angles of attack of 0° and ±6°. Maximum aeropropulsive performance at a particular Mach number was highly dependent on the operating nozzle pressure ratio. For example, as the nozzle upper ramp length or angle increased, some nozzles had higher performance at a Mach number of 0.90 because the nozzle design pressure was the same as the operating pressure ratio. Thus, selection of the various nozzle geometric parameters should be based on the mission requirements of the aircraft. A combination of large upper ramp and large lower flap boattail angles produced greater nozzle drag coefficients at Mach numbers greater than 0.80, primarily from shock-induced separation on the lower flap of the nozzle. At static conditions, the convergent nozzle had high and nearly constant values of resultant thrust ratio over the entire range of nozzle pressure ratios tested. However, these nozzles had much lower aeropropulsive performance than the convergent-divergent nozzle at Mach numbers greater than 0.60.

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Summary

An investigation has been conducted in the Langley 16-Foot Transonic Tunnel to determine the effects of varying six nozzle geometric parameters on the internal and aeropropulsive performance characteristics of single-expansion-ramp nozzles. The single-expansion-ramp nozzle is a nonaxisymmetric, variable-area, internal/external expansion exhaust nozzle. The six nozzle geometric parameters that were varied included the nozzle upper ramp length and chordal angle, the nozzle lower flap length and chordal angle, and the axial and vertical locations of the nozzle throat. Both convergent-divergent and convergent nozzle configurations were tested. Some limited tests were made to study the effects of varying the curvature of the upper ramp internal and external surfaces and the lower flap external surface of one of the nozzles. This investigation was conducted at Mach numbers from 0.60 to 1.20, nozzle pressure ratios from 1.5 to 12, and angles of attack of 0° and ±6°.

Maximum aeropropulsive performance at a particular Mach number was highly dependent on the operating nozzle pressure ratio. For example, as the nozzle upper ramp length or angle increased, some nozzles had higher performance at a Mach number of 0.90 because the nozzle design pressure was the same as the operating pressure ratio. Thus, selection of the various nozzle geometric parameters should be based on the mission requirements of the aircraft. A combination of large upper ramp and large lower flap boattail angles produced greater nozzle drag coefficients at Mach numbers greater than 0.80, primarily from shock-induced separation on the lower flap of the nozzle. At static conditions, the convergent nozzle had high and nearly constant values of resultant thrust ratio over the entire range of nozzle pressure ratios tested. However, these nozzles had much lower aeropropulsive performance than the convergent-divergent nozzle at Mach numbers greater than 0.60.

Introduction

Many studies have been made of the integration of nonaxisymmetric nozzles into fighter aircraft configurations. Nonaxisymmetric nozzle designs are generally more amenable to the incorporation of thrust vectoring to provide forces and moments for additional capabilities in aircraft maneuver and control. A prerequisite for the evolution of practical nozzles for production aircraft is the establishment of both an internal and aeropropulsive performance data base documenting the effects of nozzle internal and external geometry changes so that efficient nozzles can be selected.

One of the nonaxisymmetric nozzle types is the single-expansion-ramp nozzle (SERN). The SERN is a configuration originally developed with a hood-type jet deflector stowed in the expansion ramp which would be deployed to provide high vector angles (up to 110°) for vertical takeoff and landing operations (refs. 1 and 2). Most experimental investigations conducted on the SERN have concentrated on the uninstalled and installed performance of a specific nozzle design at various nozzle power settings during cruise and vectored-thrust operating modes. (See refs. 3 to 6.) Although studies of the effects of systematic changes in nozzle geometry on internal performance have been conducted (refs. 7 to 11), no similar investigations of aeropropulsive performance have been undertaken. This paper summarizes the aeropropulsive performance characteristics of SERN configurations having various combinations of six parameters that affect the internal and external geometry of the nozzle. These parameters include the nozzle upper ramp length and chordal angle, the nozzle lower flap length and chordal angle, and the axial and vertical locations of the nozzle throat. Both convergent-divergent and convergent nozzles were tested. This investigation was conducted in the Langley 16-Foot Transonic Tunnel at Mach numbers from 0.60 to 1.20, nozzle pressure ratios from 1.5 to 12, and angles of attack of 0° and ±6°.

Symbols

All model forces and moments are referred to the stability axis system with the model moment reference center located at model station 29.39. A discussion of the data reduction procedure, definitions of the aerodynamic force and moment terms, and the propulsion relationships used herein are presented in references 12 and 13. The symbols used in the computer-generated tables are given in parentheses.

A_e	nozzle exit area, in^2
A_i	internal area at metric break, in^2
A_m	maximum model cross-sectional area, 40.635 in^2
A_t	nozzle throat area, in^2

$(A_e/A_t)_e$		external expansion ratio for ideally expanded flow (where A_e is vertical displacement between end of nozzle ramp and lower flap times the nozzle width)
$(A_e/A_t)_i$		internal expansion ratio for ideally expanded flow (where A_e is measured in vertical plane at end of nozzle lower flap)
$C_{D,n}$	(CDN)	nozzle drag coefficient
	(CFI)	ideal thrust coefficient, $\frac{F_i}{p_a A_m}$ or $\frac{F_i}{q_\infty A_m}$
	(CFJ)	thrust coefficient along body axis, $\frac{F_j}{p_a A_m}$
	(CFN)	jet normal-force coefficient, $\frac{F_N}{p_a A_m}$
	(C(F-D))	thrust-minus-drag coefficient, $\frac{F-D}{q_\infty A_m}$
C_L	(CL)	total centerbody/nozzle lift coefficient, including thrust component, $\frac{\text{Lift}}{q_\infty A_m}$
$C_{L,n}$	(CLCN)	centerbody/nozzle lift coefficient, $C_{L,n} \equiv C_L$ at NPR = 1 (jet off)
C_m	(CM)	total centerbody/nozzle pitching moment, including thrust component, $\frac{\text{Pitching moment}}{\bar{c} q_\infty A_m}$
$C_{m,n}$	(CMCN)	centerbody/nozzle pitching moment, $C_{m,n} \equiv C_m$ at NPR = 1 (jet off)
	(CMJ)	jet pitching-moment coefficient, $\frac{\text{Jet pitching moment}}{p_a A_m}$
$C_{p,f}$		lower flap pressure coefficient, $\frac{p_f - p_\infty}{q_\infty}$
$C_{p,r}$		upper ramp pressure coefficient, $\frac{p_r - p_\infty}{q_\infty}$
$\bar{c}$		reference length (width of model at nozzle connect station), 6.800 in.
D	(D)	total centerbody/nozzle drag, lbf
D_f		centerbody friction drag, lbf
D_n	(DN)	nozzle drag, lbf
F	(F)	thrust along stability axis, lbf
F_A		axial force, lbf
$F_{A,\text{bal}}$		total force measured by force balance, lbf
$F_{A,\text{mom}}$		momentum tare axial force due to bellows, lbf
F_i	(FI)	ideal isentropic gross thrust, lbf
F_j	(FJ)	measured thrust along body axis, lbf
F_N		measured jet normal force, lbf
F_r	(FR)	resultant gross thrust, $\sqrt{F_j^2 + F_N^2}$, lbf
g		gravitational constant ($1g \approx 32.174$ ft/sec^2)
$h_{t,n}$		nominal nozzle throat height, 1.24 in.
L_n		total length of upper ramp, in.
l_f		axial length of lower flap (see fig. 2), in.
l_r		axial length of upper expansion ramp (see fig. 2), in.
M	(M)	free-stream Mach number
NPR	(NPR)	nozzle pressure ratio, $p_{t,j}/p_a$ or $p_{t,j}/p_\infty$
p_a		ambient pressure, psi
p_f		lower flap static pressure, psi
p_i		average internal static pressure, psi
p_r		upper ramp static pressure, psi
$p_{t,j}$		average jet total pressure, psi

p_∞		free-stream static pressure, psi
q_∞		free-stream dynamic pressure, psi
R		specific gas constant, 1716 ft^2/sec^2-°R
$T_{t,j}$		average jet total temperature, °R
W		width of body, in.
w_i	(WI)	ideal weight-flow rate, lbf/sec
w_p	(WP)	measured weight-flow rate, lbf/sec
x		axial distance measured from nozzle connect station (positive downstream), in.
x_t		axial distance of nozzle throat from nozzle connect station (see fig. 2(a)), in.
x'		axial coordinate of nozzle sidewall (see fig. 2(b)), in.
y		vertical distance measured from horizontal model centerline (positive upward), in.
y_t		vertical distance of nozzle throat from horizontal centerline (see fig. 2(a)), in.
z		lateral distance measured from horizontal model centerline (positive to right), in.
z'		lateral coordinate of nozzle sidewall (see fig. 2(b)), in.
α	(ALPHA)	angle of attack, deg
β_f		lower flap chordal boattail angle (see fig. 2), deg
β_r		upper ramp chordal boattail angle (see fig. 2), deg
γ		ratio of specific heats (1.3997 for air)
δ_p	(DELTAP)	resultant pitch-vector angle, $\tan^{-1} F_N/F_j$
θ_f		lower flap chordal angle (see fig. 2), deg
θ_r		upper ramp chordal angle (see fig. 2), deg

Subscripts:

des	design
e	external
i	internal

Abbreviations:

C-D	convergent-divergent
rad	radius
SERN	single-expansion-ramp nozzle
Sta.	fuselage station (axial location described by distance from model nose), in.

Nozzle Designs

The single-expansion-ramp nozzle (SERN) is a nonaxisymmetric, variable-area, internal/external expansion exhaust system. Basic SERN nozzle components consist of (1) a two-dimensional upper ramp in which a portion of the flap surface downstream of the throat serves as an external expansion ramp, and (2) a relatively short two-dimensional lower flap. In some SERN designs, nozzle power setting (throat area) can be changed by varying the geometry of the convergent-divergent upper ramp assembly (refs. 1, 2, and 6). Nozzle expansion ratio can be varied by rotation of the lower flap. In other designs the upper ramp is either fixed or has only a variable geometry downstream of the nozzle throat. The lower flap may also be fixed, but generally it is variable to provide both power setting and expansion ratio control (refs. 3 to 5). Most SERN designs also provide for thrust vectoring capability through rotation of the entire external ramp surface in conjunction with rotation of the lower flap (refs. 2, 6, and 10). The SERN configurations of the present investigation represented nominally unvectored dry-power (cruise) nozzles.

A sketch of a typical nozzle configuration is presented in figure 1. Six geometric parameters were chosen to define a nozzle because these parameters were thought to have the largest effect on both internal and external nozzle performance. The nozzle geometric parameters that were varied for the upper ramps and lower flaps are illustrated in figure 1(a) and are listed in table 1 for each of the nozzles tested. The nozzle geometric parameters that affect nozzle internal performance include upper ramp length l_r and chordal angle θ_r, and lower flap length l_f and chordal angle θ_f. The geometric parameters that affect nozzle external performance are the axial location x_t and the vertical location y_t of the nozzle throat. Of special note is that varying the ramp and flap geometric parameters can also affect nozzle external performance because variation of these parameters can change the respective nozzle boattail angles. The upper ramp and lower flap external surfaces were defined by cubic curves from the nozzle connect station to the nozzle trailing edge. The ramp inner flow path from the nozzle connect station to just past the throat is defined by a series of straight lines and circular arcs. From just past the throat to the trailing edge, the ramp inner flow path is defined by a cubic curve. The flap inner flow path is defined by a series of straight lines and circular arcs. Coordinates for all the nozzles tested are presented in table 2.

All the nozzles had full sidewalls, and a typical sidewall is shown in figure 1(b). Each of the nozzles had a pair of sidewalls in which the upper contour matched the contour of the upper ramp. Likewise, the lower contour of each of the sidewalls matched the lower flap contour. All the sidewalls had the same boattail geometry along the plane of the nozzle centerline as illustrated in figure 1(b).

For nozzles 1 to 13, the six geometric parameters were varied one at a time, whereas for nozzles 14 to 29, these parameters were varied two at a time. These parameters were varied in a controlled manner as described in reference 14 so that empirical relations for the prediction of internal and external performance could be established for nozzles having a large number of geometric parameters. Nozzles 30 to 32 were defined by varying the curvature of either the upper ramp internal and external surfaces or the flap external surface relative to that of nozzle 1. Nozzles 33 to 37 were convergent SERN nozzles; that is, the internal expansion ratio of these nozzles was 1.0.

Apparatus and Procedure

Single-Engine Propulsion Simulation System

A sketch is presented in figure 2 of the single-engine air-powered simulation system with a typical nozzle configuration installed. The model was composed of three major sections: a nose-forebody section, a centerbody section, and the nozzle. The nose-forebody section up to station 26.50 was nonmetric; that is, it was not attached to the strain gauge balance. Geometric details of this nose-forebody section can be found in reference 15. The centerbody section was made up of the low-pressure plenum, instrumentation section, and transition section (ref. 15). The centerbody section from stations 26.50 to 55.05 was essentially rectangular in cross section and had a constant width and height of 6.80 in. and 6.20 in., respectively. All sections downstream of station 26.50 were metric and mounted on the force balance. A flexible DuPont Teflon strip inserted into a circumferentially machined groove between the nose-forebody section and low-pressure plenum impeded flow into or out of the internal cavity.

An external high-pressure air system provided a continuous flow of clean, dry air at a controlled temperature of about 540°R at the nozzles. This high-pressure air was brought through the support strut by six tubes that connect to a high-pressure plenum chamber. As shown in figure 3, the air was then discharged perpendicularly into the model low-pressure plenum through eight multiholed sonic nozzles equally spaced around the high-pressure plenum. This method was designed to minimize any forces imposed by the transfer of axial momentum as the air passed from the nonmetric high-pressure plenum to the metric (mounted on the force balance) low-pressure plenum. Two flexible metal bellows were used as seals and served to compensate for axial forces caused by pressurization.

The air was then passed from the model low-pressure plenum through a choke plate, an instrumentation section, and a transition section which provided a smooth flow path for the airflow from the round low-pressure plenum to the rectangular nozzle entrance. All nozzle configurations were attached to the transition section at model station 55.05 and were tested in an inverted position (ramp on bottom). A photograph showing a typical SERN (nozzle 29) installed on the single-engine propulsion simulation system is shown in figure 4.

Instrumentation

A six-component strain gauge balance was used to measure forces and moments on the model downstream of station 26.50. Flow conditions in the nozzle were determined from 10 total pressure probes and 1 total temperature probe located at station 45.65 in the instrumentation section aft of the choke plate. Nozzle total pressure and temperature are determined from the average of these measurements. The weight flow of the high-pressure air supplied to the exhaust nozzle was measured by a critical-flow venturi (ref. 16). Eight internal static pressures, measured at the metric break, were used to account for pressure forces at this location. All the pressures noted above were measured with individual pressure transducers.

Static pressures were measured on the external surface of six of the nozzles. The orifice locations for each of these nozzles are given in table 3. The external orifices were arranged in three rows along the top of the upper ramp and in two rows on the bottom of the lower flap. These pressures were measured with electronically scanning pressure devices.

Data Reduction

All data were recorded simultaneously on magnetic tape. Approximately 50 frames of data, taken at a rate of 10 frames per second, were taken for each data point; average values were used in data reduction computations. The average value of jet total pressure was also used in all computations. All aerodynamic coefficients were referenced to a model maximum cross-sectional area of 40.635 in^2.

The balance force measurements from which thrust is subsequently obtained are initially corrected for model weight tares and balance interactions. Although the bellows arrangement was designed to eliminate pressure and momentum effects on the balance readings, small bellows tares on all balance components still exist. These tares result from a small pressure difference between the ends of the bellows when internal velocities are high and also from small differences in the spring constants of the forward and aft bellows when the bellows are pressurized. As discussed in reference 12, these bellows tares were determined by testing calibration nozzles with a known performance over a range of nozzle pressure ratios with normal-force and pitching-moment loadings simulating the ranges expected for the test nozzles. The balance data were then corrected in a manner similar to that discussed in references 12 and 13.

At static conditions, the resultant gross thrust F_r used in the resultant thrust ratio F_r/F_i was then determined from the individual corrected forces F_j and F_N. Expansion of the flow over the surface of the upper ramp produces a resultant thrust force that is not aligned with the horizontal centerline and, hence, significant differences between F_r/F_i and F_j/F_i can occur. Resultant thrust vector angle δ_p is presented for evaluation of the exhaust-flow turning capabilities of the various nozzles tested.

Nozzle discharge coefficient w_p/w_i is the ratio of measured weight flow to ideal weight flow, where ideal weight flow is based on jet total pressure $p_{t,j}$, jet total temperature $T_{t,j}$, and measured nozzle throat area. Nozzle discharge coefficient is then a measure of the ability of the nozzle to pass mass flow and is reduced by boundary-layer thickness and nonuniform flow in the throat. Using the measured weight flow, ideal thrust of the nozzle can be computed from the equation

$$F_i = w_p \sqrt{\frac{RT_{t,j}}{g^2}\left(\frac{2\gamma}{\gamma-1}\right)\left[1-\left(\frac{1}{\mathrm{NPR}}\right)^{(\gamma-1)/\gamma}\right]}$$

At wind-on conditions, thrust minus axial force was obtained from the equation

$$F_j - F_A = F_{A,\mathrm{bal}} + (p_i - p_\infty)A_i - F_{A,\mathrm{mom}} + D_f$$

where the first term $F_{A,\mathrm{bal}}$ includes all pressure and viscous forces (internal and external on the afterbody, nozzle, and thrust system). The second term accounts for the interior pressure forces acting at the metric break. The third term $F_{A,\mathrm{mom}}$ is the momentum tare force previously discussed. The last term D_f is the friction drag of the centerbody section from stations 26.50 to 55.05. Note that this term is included in the thrust-minus-drag term $F - D$.

The adjusted forces and moments measured by the force balance are transferred from the body axis of the metric portion of the model to the stability axis. The attitude of the nonmetric forebody relative to gravity was determined from a calibrated attitude indicator located in the model nose. The angle of attack α, which is the angle between the centerbody/nozzle centerline and the relative wind, was determined by applying terms for centerbody deflection (caused when the model and balance bend under aerodynamic load) and a tunnel flow angularity term to the angle measured by the attitude indicator. The flow angularity correction was 0.1°, which is the average angle measured in the Langley 16-Foot Transonic Tunnel.

The thrust-removed (nozzle) aerodynamic forces and moments were obtained by determining the components of thrust in axial force, normal force, and pitching moment and then subtracting these values from the measured total (aerodynamic plus thrust) forces and moments. These thrust components at forward speeds were determined from measured static data and were a function of the free-stream static and dynamic pressures.

Wind Tunnel and Tests

The Langley 16-Foot Transonic Tunnel is a single-return atmospheric wind tunnel with a slotted octagonal test section and continuous air exchange. The wind tunnel has variable airspeeds up to a Mach number of 1.30, with test section plenum suction being used for speeds above a Mach number of 1.05. A complete description of this facility and operating characteristics can be found in reference 12.

This investigation was conducted at Mach numbers from 0.60 to 1.20 at nozzle pressure ratios from 1.5 to 12 depending on Mach number. All nozzle configurations were tested at an angle of attack of 0°, and selected configurations were also tested at angles of attack of −6° and 6°. External pressures were measured on six of the nozzles in separate tests. The Reynolds number per foot varied from 3.2×10^6 to 4.0×10^6. All tests were conducted with a 0.10-in-wide boundary-layer trip consisting of a strip of No. 120 silicon carbide grit sparsely distributed in a thin film of lacquer located 1.00 in. from the tip of the forebody nose.

Presentation of Results

The results of this investigation are presented in both tabular and plotted form. Internal and aeropropulsive characteristics are presented for nozzles 1 to 37 in tables 4 to 40, respectively. Skin-friction drag coefficients for each of the nozzles are given in table 41. Basic and summary data are presented as follows:

Discussion of Results

The exhaust-flow expansion process for single-expansion-ramp nozzles occurs both internally and externally. That is, internal expansion of the flow occurs from the nozzle throat up to the end of the lower flap where it is contained by the internal surfaces of the nozzle and is controlled by the internal expansion ratio $(A_e/A_t)_i$. External expansion, which occurs downstream of the lower flap trailing edge, is bounded by the expansion ramp and the free (ambient/exhaust) boundary and is controlled by the external expansion ratio $(A_e/A_t)_e$. Thus, thrust performance is influenced by internal and external expansion ratios that tend to cause two performance peaks. Because these nozzles have external expansion surfaces that can be affected by external flow, aeropropulsive performance depends on Mach number, nozzle pressure ratio, and angle of attack. In addition, expansion of the flow over the surface of the external ramp produces a resultant thrust force that is not aligned with the horizontal centerline of the nozzle and varies with nozzle pressure ratio. Therefore, when a single-expansion-ramp nozzle is integrated into an aircraft configuration, the vertical-force component on the ramp and its contribution as a pitching moment must be included as a trim control consideration.

Three types of data presentation are used to illustrate the effects of varying each of the nozzle geometric parameters. At static conditions, resultant gross thrust ratio F_r/F_i, thrust-vector angle δ_p, and nozzle discharge coefficient w_p/w_i are presented as a function of nozzle pressure ratio. Resultant gross thrust F_r is equal to axial thrust F_j along the body axis when $\delta_p = 0°$. Significant differences occur between F_r and F_i when the jet exhaust is turned from the axial direction, and the magnitudes of these differences are a function of δ_p. Comparisons of data for nozzles with variable nozzle geometric parameters at wind-on conditions are made in terms of the usual aeropropulsive performance parameter $(F - D_n)/F_i$ (used for axisymmetric nozzles) and nozzle drag coefficient $C_{D,n}$. Results are presented as a function of nozzle pressure ratio at each of the Mach numbers tested and are also summarized as a function of Mach number at a typical operating nozzle pressure ratio for each of the Mach numbers tested. This schedule of typical operating pressure ratios is given in table 42. Although discussion of the results at this schedule of NPR and Mach number would generally be applicable for other schedules, the relative difference between comparisons may vary.

Nozzle drag coefficient $C_{D,n}$ is presented for each nozzle comparison at jet off (NPR = 1) and at the same jet-on conditions as the aeropropulsive performance. At power-on conditions, nozzle drag coefficients were obtained by subtracting the static thrust components from the thrust-minus-drag measurement. Because of this procedure, any effects of the external flow on the internal performance of the nozzles are reflected as a change in nozzle drag.

Variations in SERN nozzle geometry generally result in changes in nozzle internal and/or external expansion ratio, thereby shifting the pressure ratios for optimum performance (design condition). When such geometric variations are made, performance changes are expected, but they cannot always be described as being beneficial or detrimental since the nozzles cannot be compared on equal terms at a given pressure ratio. In addition, changes to the nozzle geometry also resulted in variations to both the upper ramp and lower flap boattail angles. These variations will affect external drag characteristics.

Variation of Six Nozzle Geometric Parameters

Effect of upper ramp length. The effects of varying upper ramp length on nozzle internal and aeropropulsive performance are presented in figure 5. At static conditions (fig. 5(a)), these nozzles have nearly the same resultant thrust ratios at nozzle pressure ratios from 2 to about 4 because they have nearly the same internal expansion ratio. An NPR of 4 is slightly above the nozzle pressure ratio for optimum internal expansion (table 1). At pressure ratios above 4, peak nozzle resultant thrust is dependent on the nozzle pressure ratio for optimum external expansion. Figure 5(a) indicates that although nozzle 2 has probably reached its peak resultant thrust between an NPR of 4.5 and 5, nozzle 1 would reach its peak performance at an NPR somewhat greater than 7. Based on external expansion ratio, nozzle 3 would have its peak performance between nozzle pressure ratios of 10 and 12. As observed previously for SERN nozzles (refs. 8 to 10), the resultant thrust ratio levels remain near peak levels over a much wider range of nozzle pressure ratio than would be expected for a typical convergent-divergent nozzle. This performance characteristic, which results from the two separate-flow expansion processes (internal and external), can be a significant advantage for SERN nozzles because less (or no) expansion-ratio control may be required (particularly for an all-subsonic-mission aircraft) and reductions in exhaust-system weight and complexity can be achieved.

The nonlinear variation of resultant thrust-vector angle δ_p with nozzle pressure ratio is characteristic of SERN nozzles and is caused by the changing compression-expansion wave patterns impinging on the ramp as NPR is varied. An axial-force (body axis) performance penalty would be associated with any value of resultant thrust-vector angle that is nonzero because the resultant thrust is being turned away from the axial direction. Since the ramp has a large, unopposed, normal projected area, values of normal force can change significantly with varying nozzle pressure ratio. Typical values of jet normal-force coefficient at static conditions and of total lift coefficient at wind-on conditions are included in the data tables for each of the nozzles tested. In addition, the resulting pitching-moment coefficients, also presented in the tables, would have to be considered in trimming an aircraft configuration that employed SERN nozzles.

Characteristics of nozzle discharge coefficient are also presented in figure 5(a). Nozzle discharge coefficient w_p/w_i is a measure of the ability of the nozzle to pass mass flow, and this ability is reduced by boundary-layer thickness and nonuniform flow in the nozzle throat. Changes in nozzle geometry that occur downstream of the nozzle throat (supersonic exhaust) usually do not affect characteristics of nozzle discharge coefficient, as shown by the data in figure 5(a). The three nozzles shown in figure 5(a) have levels of w_p/w_i that are typical for this class of nozzles. As will be shown subsequently, the discharge coefficients of the other nozzles experienced little or no effect from varying the geometric parameters used to define the nozzles of this investigation. In addition, the external flow had essentially no effect on the discharge coefficient. (See the data tables.)

The variation of the aeropropulsive parameter $(F - D_n)/F_i$ and nozzle drag coefficient $C_{D,n}$ is also presented in figure 5 for Mach numbers from 0.60 to 1.20. As expected because of increased drag, the aeropropulsive performance for the three nozzles decreased with increasing Mach number. In general, the variation of nozzle drag coefficient with nozzle pressure ratio NPR for a particular nozzle is similar to that of axisymmetric nozzles, particularly at an angle of attack of 0°. Nozzle drag decreases with initial jet operation because a reduction occurs in the external flow expansion required at the nozzle exit as the exhaust flow fills the nozzle base region. This reduced expansion generally results in higher pressures on the nozzle boattail regions. This increase in nozzle boattail pressures is shown in figure 51 where external pressure distributions are given for nozzle 1. As nozzle pressure ratio is further increased, nozzle drag increases with a subsequent decrease in nozzle drag coefficient for additional increases in NPR. This variation in drag coefficient with increasing NPR is probably caused by exhaust-flow entrainment effects on the external nozzle flow at NPR = 2.5 to 4 and by a compression at the nozzle exit that is created by the increased thickness of the exhaust-flow plume at NPR > 4. These trends in nozzle drag coefficient that occur with increasing nozzle pressure ratio are typical for jet-powered models.

The effects of varying upper ramp length on the aeropropulsive parameter $(F - D_n)/F_i$ and nozzle drag coefficient $C_{D,n}$ are summarized in figures 6 and 7, respectively. Data are shown for $\alpha = 0°$ at the Mach number and NPR schedule given in table 42. The relative ranking of the three nozzles at $M = 0.60$ and 0.90 is essentially the same as that shown at static conditions (fig. 5(a)). The lower performance of nozzle 2 at $M = 0.60$ and of nozzle 1 at $M = 0.90$ may be due in part to the more negative resultant thrust-vector angle of nozzle 2 at NPR = 3 and of nozzle 1 at NPR = 5.

At $M = 0.95$ and 1.20, which have scheduled nozzle pressure ratios of 7 and 8, respectively, nozzle 1 with an intermediate external expansion ratio had the highest aeropropulsive performance. This higher performance can be attributed to the fact that nozzle 1 is operating closer to design at these pressure ratios than either nozzles 2 or 3. At Mach numbers higher than those tested, which would also have a higher scheduled NPR, nozzle 3 (with the largest external expansion ratio) is expected to have the highest aeropropulsive performance because it operates closer to design than either nozzles 1 or 2. Except for $M = 0.60$, the aeropropulsive performance ranking of nozzles 1, 2, and 3 followed that expected from static (internal) thrust trends with varying expansion ratio. At $M = 1.20$, nozzle 1 also had the same performance as nozzle 3 at NPR = 10 and slightly less performance at NPR = 12 (fig. 5(j)). At NPR = 12, nozzle 3 is slightly underexpanded and is at an NPR slightly above that for optimum internal performance ($(\text{NPR}_e)_{des} = 11.4$). This result suggests that less expansion ratio control may be required for SERN nozzles since little performance gain was achieved by increasing the external expansion ratio from 1.69 for nozzle 1 to 2.08 for nozzle 3 (for the Mach number range of the current investigation).

The aeropropulsive performance level of nozzle 2 at $M = 1.20$ is significantly lower than that of either nozzles 1 or 3 (fig. 6). This lower performance for nozzle 2 results from a much higher drag coefficient at this Mach number (fig. 7). Since this configuration had the steepest upper ramp boattail angle, it probably had higher boattail pressure drag. In addition, nozzle wave drag could be higher than that of the other two nozzles because of its lower fineness ratio.

At Mach numbers from 0.60 to 0.90, increasing the upper ramp length increased the jet-off nozzle drag coefficient (fig. 7). Part of this increase (less than 20 percent) is due to differences in the skin-friction drag coefficient. (See table 41.) Figure 7 also shows small differences in the jet-on drag coefficients for all three nozzles at these same Mach numbers. At jet-off conditions, one should note that the drag on the expansion surface of the ramp is included in the nozzle drag coefficient term and, for this nozzle comparison, can be large for nozzle 3 which has the longest ramp. For $M > 0.90$, nozzle drag coefficient increased with decreasing ramp length (increasing ramp boattail angle), particularly at $M = 1.20$.

These three nozzles were also tested at angles of attack of −6° and 6°, and the variation of the aeropropulsive parameter and nozzle drag coefficient with angle of attack is summarized in figures 8 and 9, respectively. In general for these three nozzles, $(F - D_n)/F_i$ increased as α was increased from $-6°$ to $6°$. This increase was due mainly to a decrease in nozzle drag as angle of attack was increased (fig. 9).

Effect of upper ramp angle. The effects of varying upper ramp chordal angle on nozzle internal and aeropropulsive performance are shown in figure 10. As can be seen in figure 10(a), increasing the upper ramp chordal angle θ_r lowered the resultant thrust ratio F_r/F_i over the entire NPR range tested. Similar results were obtained in reference 9 which also showed that the decrease in resultant thrust ratio resulted from a reduction in pressures on the upper ramp.

At subsonic Mach numbers, increasing the upper ramp angle also resulted in a decrease in aeropropulsive performance (figs. 10(b) to 10(e) and 11). With the absence of pressure measurements, assessing the extent of external flow effects on nozzle internal performance is difficult. However, for nozzle 5, these external flow effects may be detrimental because this nozzle had a much higher jet-on nozzle drag coefficient than either nozzles 1 or 4 (figs. 10(b) to 10(e) and 11). Lower nozzle drag was expected for nozzle 5 because it had the smallest upper ramp boattail angle. As noted previously, any external flow effects on nozzle internal (thrust) performance are charged to nozzle drag coefficient. Nozzle 4, which had the lowest ramp chordal angle, provided the highest aeropropulsive performance at subsonic Mach numbers below 0.95 (fig. 11). This result was caused by two factors. First, nozzle 4 generally had the lowest nozzle drag coefficient in this speed range (fig. 12), and second, this nozzle was operating close to its design nozzle pressure ratio in this speed range. (See tables 1 and 42.) These results indicate that the nozzle should be designed to operate at low values of upper ramp angles for the Mach numbers and nozzle pressure ratios tested. For those applications in which a variable upper ramp is required, such as for thrust vectoring, the upper ramp angle could be varied in order to optimize nozzle performance over a wide range of operating conditions.

Nozzle 1 had the highest aeropropulsive performance at $M = 1.20$ of the three nozzles shown because it is operating near design pressure ratio. As seen in figure 10(f), the aeropropulsive performance for nozzle 1 was essentially the same as that of nozzle 5 at nozzle pressure ratios from 10 to 12 even though the design NPR for nozzle 5 was 10.65. This again indicates that SERN nozzles may operate efficiently at a lower expansion ratio than that required for full expansion of the exhaust flow.

Effect of lower flap length. The effects of varying lower flap length on nozzle internal and aeropropulsive performance and nozzle drag are shown in figures 13 to 15. At NPR > 3, there was an increase in nozzle internal performance as the lower flap length increased (fig. 13(a)) although the differences between nozzles 1 and 7 were small. Similar effects were found in references 8 and 9 where reference 9 showed that as the lower flap length was increased, pressures on the upper ramp also increased which would be expected to result in higher internal performance. In addition, reference 9 indicated that at nozzle pressure ratios below $(\mathrm{NPR}_i)_{\mathrm{des}}$, shock waves with some flow separation were present on the ramp and that these shocks moved farther upstream on the ramp as lower flap length was decreased. Consequently, these results imply that for nozzle 6, which has the shortest lower flap, more of the ramp is probably separated which would contribute to the lower internal performance for this nozzle when compared with that of nozzles 1 and 7. In addition, nozzle 6 had the lowest internal design nozzle pressure ratio of those shown in figure 9, and thus it was operating farther off design at NPR > 3 than nozzles 1 and 7.

At forward speeds, nozzle 1 generally had higher aeropropulsive performance than either nozzles 6 or 7 at NPR > 3 for all Mach numbers tested (figs. 13 and 14). The effects of varying lower flap length on nozzle drag (figs. 13 and 15) were small, but in general, nozzle 1 had the lowest nozzle drag.

A comparison of external pressure distributions between nozzles 1, 6, and 8 is presented in figure 52. In general, small differences exist between the pressure distributions over the external ramp. Nozzle 6 tends to have a slightly higher pressure recovery at jet-off conditions. On the lower flap, nozzle 6 had a greater expansion of the flow over the initial portion of the lower surface coupled with higher downstream pressure recovery than that of nozzle 1. The generally higher nozzle drag coefficient for nozzle 6 may in part be due to this region of low (negative) pressure coefficients acting on the larger axial projected area of the initial lower flap curvature.

Effect of lower flap angle. The effects of varying lower flap angle on nozzle internal and aeropropulsive performance and nozzle drag coefficient are presented in figures 16 to 18. The internal expansion ratios for these three nozzles differ widely, and as a result, peak internal performance occurs at different nozzle pressure ratios that correspond to the various expansion ratios (fig. 16(a)).

The effects of varying lower flap angle on aeropropulsive performance, shown in figure 17, follow expected trends based on the results from static conditions. Nozzle 9 has the highest resultant thrust ratio at NPR = 3 (fig. 16(a)) and the highest performance at $M = 0.60$. At $M = 1.20$, nozzle 8 has higher performance than nozzle 9 because nozzle 8 is now operating at an NPR closer to that required for optimum external expansion and also because nozzle 9 has a higher nozzle drag. However, the highest performance at $M = 1.20$ was obtained with nozzle 1 which is operating near design for external expansion (that is, $(\mathrm{NPR})_{\mathrm{des}} = 7.84$ versus scheduled NPR = 8 at $M = 1.20$) and also has lower nozzle drag than nozzle 9 and slightly lower drag than nozzle 8. For a fully variable SERN, the lower flap would most likely be movable, and thus the lower flap angle along with the upper ramp angle would be varied to maximize performance over a wide range of flight conditions. The effect of varying lower flap angle on nozzle drag coefficient (fig. 18) was small except for nozzle 9 which had significantly higher nozzle drag at $M = 0.95$ and 1.20.

A comparison of the external pressure distributions between nozzles 1 and 8 is presented in figure 52. Little or no difference occurred in the pressure distributions over the range of conditions tested.

Effect of axial location of nozzle throat. The effects of varying the axial location of the nozzle throat on internal and aeropropulsive performance and nozzle drag coefficient are shown in figures 19 to 21. The axial location of the nozzle throat will probably have the most impact on the external performance of the nozzle because both the upper ramp and lower flap boattail angles can be significantly changed by varying this geometric parameter while internal and external expansion ratios remain constant. For example, low nozzle boattail angles, which are desirable for nozzles installed in an aircraft designed to cruise supersonically, would result as the axial location of the throat is increased.

As expected, figure 19(a) shows that the three nozzles have essentially the same internal performance. Thus, any changes to aeropropulsive performance from varying the axial location of the nozzle throat should be due to external flow effects only and will show up primarily as changes to nozzle drag coefficient.

As can be seen in figure 21, nozzles 10 and 11 had the same jet-off drag coefficients (somewhat higher than $C_{D,n}$ for nozzle 1) at $M = 0.60$ and 0.80. At a scheduled NPR for these Mach numbers, the drag coefficient for nozzle 11 was considerably higher than that of either nozzles 1 or 10. This higher drag coefficient was due to less favorable jet effects because nozzle 11 had the same jet-off drag coefficient as nozzle 10.

The jet-off nozzle drag coefficient for nozzle 10 at $M = 0.90$ to 1.20 was significantly higher than that of either nozzles 1 or 11, and in fact, nozzle 10 had one of the highest jet-off drag coefficients measured of all the nozzles tested. This higher drag is believed to be a result of an increase in pressure drag due to the greater upper ramp and lower flap boattail angles of nozzle 10.

External pressures of nozzles 1 and 10 are presented in figure 53. At $M = 0.60$ and 0.80, nozzle 10 had a greater expansion of the flow over both the ramp and flap with recovery of the flow to higher positive pressures than nozzle 1. This greater recompression of the flow on nozzle 10 results in lower drag (or possibly a thrust) on the aft portion of the nozzle external surfaces which can compensate for the higher drag caused by the greater expansion (lower pressures) of the flow over the initial boattail surfaces. This would account for the small difference in nozzle drag between nozzles 10 and 1.

At Mach numbers greater than 0.80, a marked difference was evident in the flow characteristics between nozzles 1 and 10, particularly on the lower flap. Although the flow over the upper ramp of nozzle 10 still expands more than on nozzle 1, the flow now recompresses to the same levels as nozzle 1. On the lower flap, nozzle 10 now has a strong expansion that is followed by a shock and flow separation. This flow separation is quite evident at $M = 0.95$ at both jet-off and jet-on conditions (figs. 53(k) to 53(m)). These results would indicate that at subsonic speeds, larger lower flap boattail angles are probably undesirable.

The fact that nozzle 11 had the lowest drag coefficient at $M = 1.20$ (fig. 21) was probably due to lower pressure drag because of the lower boattail angles of both the upper ramp and lower flap. Consequently, nozzle 11 had the highest aeropropulsive performance of all nozzles tested at $M = 1.20$.

The effects of angle of attack on aeropropulsive performance and nozzle drag are summarized in figures 22 and 23, respectively, for nozzles 1, 10, and 11. Generally, angle of attack had no effect on the relative differences between the three nozzles. That is, at $M = 0.60$ nozzles 1 and 10 had the same performance but nozzle 11 had lower performance for the three angles of attack tested.

Effect of vertical location of nozzle throat. The effects of varying the vertical location of the nozzle throat on internal and aeropropulsive performance and nozzle drag coefficient are presented in figures 24 to 26. Varying the vertical location of the nozzle throat is a means of trading upper or lower nozzle boattail angles without having to make large extensions to the upper ramp or lower flap. Since all three nozzles had identical expansion ratios, it was expected that nozzles 1, 12, and 13 would have the same internal performance, but, as can be seen in figure 24(a), nozzle 13 had lower resultant thrust ratios up to NPR = 4. Because peak internal performance occurred at NPR $\approx$ 4.5 for nozzle 13 (versus NPR $\approx$ 3.5 for nozzles 1 and 12), it is apparent that this nozzle had a higher effective expansion ratio. The reason for this shift in effective expansion ratio is not known, but it may be due to the convergent section of the upper ramp being too short. As a result, performance at forward speeds is governed by both internal and external flow characteristics.

In general, the effects of varying the vertical location of the nozzle throat on either the aeropropulsive parameter or nozzle drag coefficient are small (figs. 25 and 26). Nozzle 13 had lower aeropropulsive performance at $M = 0.60$ than either nozzles 1 or 12 (fig. 25) because this nozzle had lower internal performance at the scheduled NPR of 3 (fig. 24(a)). An upward movement of the nozzle throat location (nozzles 12 and 13) resulted in an increase in lower flap boattail angle and a decrease in upper ramp angle. As might be expected, these opposing changes in boattail angle appeared to contribute opposite effects on external drag so that the net effect on drag coefficient was small (fig. 26).

Effect of Surface Shape Variations

Some limited tests were conducted in order to determine the effect of varying the shape of some of the nozzle surfaces without changing the basic six geometric parameters that defined a particular nozzle. This was accomplished on nozzle 1 by varying the curvature of either the upper ramp internal or external surfaces or the lower flap external surface which resulted in nozzles 30 to 32.

Effect of upper ramp expansion surface. The effects of varying the curvature of the upper ramp expansion surface on the internal and aeropropulsive performance and nozzle drag coefficients are presented in figures 27 to 29. The modification to the ramp expansion surface consisted of reducing the curvature of the surface relative to nozzle 1 which reduced the initial ramp expansion angle. As shown in figure 27(a), reducing the ramp curvature caused essentially no effect on the resultant thrust ratio; however, resultant thrust-vector angles were more negative. Similar effects of ramp curvature on thrust-vector angle were reported in reference 9.

At subsonic speeds the aeropropulsive performance of nozzle 30 was slightly higher than that of nozzle 1 (fig. 28) because nozzle 30 generally had lower nozzle drag coefficients at these speeds (fig. 29). This change in nozzle drag coefficient was most likely due to a favorable external-flow effect on the nozzle internal performance rather than to external drag characteristics because there was no geometric change to the external surfaces of the two nozzles. In general, the difference in either aeropropulsive performance or nozzle drag coefficient between nozzles 1 and 30 remained the same at each of the angles of attack tested. (Compare the results of tables 4 and 33.)

Effect of varying upper ramp external surface. The effects of varying the shape of the upper ramp external surface on the internal and aeropropulsive performance and nozzle drag characteristics are presented in figures 30 to 32. As expected, changing the curvature of the upper ramp external surface caused no effect on internal performance (fig. 30(a)). At subsonic speeds, nozzle drag coefficient was generally lower for nozzle 31 than for nozzle 1 (fig. 32), and as a result, nozzle 31 had higher aeropropulsive performance than nozzle 1 up to $M = 0.80$ (fig. 31). Angle-of-attack effects (not shown graphically) were similar to those discussed previously for the upper ramp expansion surface. (Compare tables 4 and 34.)

Effect of varying lower flap external shape. The effects of varying the lower ramp external shape on internal and aeropropulsive performance and nozzle drag coefficients are shown in figures 33 to 35. The internal resultant thrust ratio for nozzle 32 was higher than that for nozzle 1 even though these two nozzles were supposed to have the same internal contours. (Compare tables 2(a) and 2(ff).) This difference in internal performance is attributed to geometry differences between the lower flaps for nozzles 1 and 32 because both nozzles used the same upper ramp piece when configured as a test nozzle. As shown in figure 35, nozzle 32 had higher jet-off and jet-on nozzle drag than nozzle 1, in particular at Mach numbers greater than 0.90. As a result, this nozzle generally had poorer aeropropulsive performance characteristics than nozzle 1 (figs. 33 and 34). The results at angle of attack (not shown) are once again the same as those already noted. (Compare tables 4 and 35.)

Variation of Convergent Single-Expansion-Ramp Nozzles

Several convergent SERN's were investigated in order to determine the effects of nozzle geometry variations on both internal and aeropropulsive performance of these types of nozzles. A convergent SERN is still an external expansion nozzle but the internal expansion ratio is 1 and all expansion occurs downstream of the lower flap exit. The use of a convergent SERN may be desirable for an all-subsonic-mission aircraft where little expansion ratio control may be required. For a fixed-geometry nozzle, significant reductions in exhaust-system weight and complexity can be achieved.

Effect of upper ramp length. The effects of varying nozzle upper ramp length on the internal and aeropropulsive performance and on nozzle drag coefficient are presented in figures 36 to 38. Increasing the upper ramp length decreased resultant thrust ratios at NPR > 2.5 up to the maximum NPR tested (fig. 36(a)). Note that these two nozzles maintained nearly constant levels of resultant thrust ratio over the entire range of nozzle pressure ratios tested. This characteristic trend of internal performance for convergent SERN nozzles has been shown previously in reference 11. Aeropropulsive performance was lower for nozzle 33 (with a long upper ramp) up to $M = 0.90$ (figs. 36 and 37) primarily because nozzle 34 has better thrust characteristics. However, at $M = 0.95$ and 1.20, nozzle 33 has higher aeropropulsive performance because this nozzle has lower nozzle drag coefficients than nozzle 34 (figs. 36 and 38). The higher nozzle drag for nozzle 34 probably results from higher boattail drag that develops on the shorter upper ramp of this nozzle at transonic and supersonic speeds.

Effect of upper ramp angle. The internal and aeropropulsive performance and nozzle drag characteristics that result from varying the nozzle upper ramp angle are shown in figures 39 to 41. At subsonic speeds, increasing the upper ramp angle generally decreased the resultant thrust ratio and aeropropulsive performance parameter and increased the nozzle drag coefficient. Similar effects were found for the convergent-divergent nozzles (figs. 10 to 12). At $M = 1.20$, increasing the upper ramp angle increased aeropropulsive performance at a nozzle pressure ratio near the scheduled value of 8 because of lower nozzle drag.

Effect of lower flap length. The effects of increasing lower flap length on internal and aeropropulsive performance and nozzle drag coefficient are presented in figures 42 to 44. Increasing the lower flap length resulted in a significant increase in resultant thrust ratio of about 2 percent over the entire range of nozzle pressure ratio tested (fig. 42(a)). This result is similar to that of reference 11 which also found that variation of this parameter had the largest effects on nozzle internal performance. Nozzle 36 (with a long lower flap) also had higher aeropropulsive performance than nozzle 37 (fig. 43) over the entire Mach number range tested because nozzle 36 had higher static (or internal) performance.

Effect of lower flap angle. The effects of varying the lower flap angle on internal and aeropropulsive performance are presented in figures 45 to 47. Decreasing the lower flap angle, that is, going from $-4.00°$ (nozzle 33) to $-18.00°$ (nozzle 37), resulted in small increases in both resultant thrust ratio (fig. 45(a)) and aeropropulsive performance (fig. 46). Similar results were found for the convergent-divergent SERN nozzles.

Comparison between convergent and convergent-divergent nozzles. A comparison of internal and aeropropulsive performance between convergent and convergent-divergent SERN nozzles is presented in figures 48 to 50. Nozzle 36 (convergent) had higher internal performance than nozzle 1 (C-D) up to about NPR $= 3.5$ (which is about the nozzle pressure ratio for optimum internal expansion for nozzle 1) and then essentially the same performance up to about NPR $= 7$. (Both nozzles had the same external expansion ratios.) Except for NPR < 2.5, nozzle 33 (convergent) had the lowest internal performance of the three nozzles considered.

A comparison of aeropropulsive performance between the convergent-divergent and the convergent SERN nozzles is shown in figure 49. At the scheduled NPR (3) for $M = 0.60$, nozzle 36 (convergent) had the highest aeropropulsive performance mainly because this nozzle had the highest internal performance at this nozzle pressure ratio. However, at all other test conditions, nozzle 1 (C-D) had significantly higher performance than either of the convergent SERN nozzles, 33 or 36. Part of this lower performance is attributed to these two nozzles having much higher nozzle drag at both jet-off and jet-on conditions (fig. 50). The reason for this much higher drag for both nozzles 33 and 36 is evident in the pressure distributions presented in figure 54. The flow over the upper ramp for all three nozzles has essentially the same characteristics showing similar initial expansion of the flow followed by a strong pressure recovery to relatively high positive pressures. However, the expansion of the flow over the lower flap for both nozzles 33 and 36 is much stronger with the expansion peak occurring farther downstream on the flap. At Mach numbers greater than 0.60, evidence exists of a shock on the flap with some resulting shock-induced separation, especially at $M = 0.90$ and 0.95. At these conditions, the initial expansion of the flow over the lower flap of nozzle 1 is followed by a strong recovery to positive pressures which, acting on the aft-facing nozzle boattail, will decrease nozzle drag relative to the other nozzles.

Concluding Remarks

An investigation conducted in the Langley 16-Foot Transonic Tunnel has determined the effects of varying six nozzle geometric parameters on the internal and aeropropulsive performance characteristics of single-expansion-ramp nozzles. These parameters included the nozzle upper ramp length and chordal angle, the nozzle lower flap length and chordal angle, and the axial and vertical locations of the nozzle throat. Both convergent-divergent and convergent nozzle configurations were tested. Some limited tests were made to study the effects of varying the curvature of the upper ramp internal and external surfaces and the lower flap external surface of one of the nozzles. This investigation was conducted at Mach numbers from 0.60 to 1.20, nozzle pressure ratios from 1.5 to 12 and angles of attack of $0°$ and $\pm 6°$.

Maximum aeropropulsive performance at a particular Mach number was highly dependent on the operating nozzle pressure ratio. For example, as upper ramp length or angle increased, some nozzles had higher performance at a Mach number of 0.90 because the nozzle design pressure ratio was the same as the operating pressure ratio. Thus, selection of the various nozzle geometric parameters should be based on the mission requirements of the aircraft. Because of the two separate exhaust-flow expansion processes, less external expansion ratio control may be required for single-expansion-ramp nozzles. A combination of large upper ramp and large lower flap boattail angles produced greater nozzle drag coefficients at Mach numbers greater than 0.80, primarily from shock-induced separation on the lower flap of the nozzle. An increase in both resultant thrust ratio and aeropropulsive performance resulted from increasing the flap length of the convergent single-expansion-ramp nozzle. At static conditions, the convergent nozzles had high and nearly constant values of resultant thrust ratio over the entire range of nozzle pressure ratio tested. However, these nozzles had much lower aeropropulsive performance than the convergent-divergent nozzle at Mach numbers greater than 0.60. Varying the external shape of either the upper ramp or the lower flap of one of the nozzles resulted in small changes to either aeropropulsive performance or nozzle drag.

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Table 1. Nozzle Geometric Parameters

Table 2. Nozzle Coordinates

[The symbol s denotes a straight-line contour between two indicated coordinates]

(a) Nozzle 1

Table 2. Continued

(b) Nozzle 2

Table 2. Continued

(c) Nozzle 3

Table 2. Continued

(d) Nozzle 4

Table 2. Continued

(e) Nozzle 5

Table 2. Continued

(f) Nozzle 6

Table 2. Continued

(g) Nozzle 7

Table 2. Continued

(h) Nozzle 8

Table 2. Continued

(i) Nozzle 9

Table 2. Continued

(j) Nozzle 10

Table 2. Continued

(k) Nozzle 11

Table 2. Continued

(l) Nozzle 12

Table 2. Continued

(m) Nozzle 13

Table 2. Continued

(n) Nozzle 14

Table 2. Continued

(o) Nozzle 15

Table 2. Continued

(p) Nozzle 16

Table 2. Continued

(q) Nozzle 17

Table 2. Continued

(r) Nozzle 18

Table 2. Continued

(s) Nozzle 19

Table 2. Continued

(t) Nozzle 20

Table 2. Continued

(u) Nozzle 21

Table 2. Continued

(v) Nozzle 22

Table 2. Continued

(w) Nozzle 23

Table 2. Continued

(x) Nozzle 24

Table 2. Continued

(y) Nozzle 25

Table 2. Continued

(z) Nozzle 26

Table 2. Continued

(aa) Nozzle 27

Table 2. Continued

(bb) Nozzle 28

Table 2. Continued

(cc) Nozzle 29

Table 2. Continued

(dd) Nozzle 30

Table 2. Continued

(ee) Nozzle 31

Table 2. Continued

(ff) Nozzle 32

Table 2. Continued

(gg) Nozzle 33

Table 2. Continued

(hh) Nozzle 34

Table 2. Continued

(ii) Nozzle 35

Table 2. Continued

(jj) Nozzle 36

Table 2. Concluded

(kk) Nozzle 37

Table 4. Static and Aeropropulsive Characteristics for Nozzle 1

[Parts (c) and (d) come from repeat runs]

(a) Internal performance

(b) Aeropropulsive characteristics

Table 4. Concluded.

(c) Internal performance

(d) Aeropropulsive characteristics

Table 5. Internal and Aeropropulsive Characteristics for Nozzle 2

(a) Internal performance

(b) Aeropropulsive characteristics

Table 6. Internal and Aeropropulsive Characteristics for Nozzle 3

(a) Internal performance

(b) Aeropropulsive characteristics

Table 7. Internal and Aeropropulsive Characteristics for Nozzle 4

(a) Internal performance

(b) Aeropropulsive characteristics

Table 8. Internal and Aeropropulsive Characteristics for Nozzle 5

(a) Internal performance

(b) Aeropropulsive characteristics

Table 9. Internal and Aeropropulsive Characteristics for Nozzle 6

(a) Internal performance

(b) Aeropropulsive characteristics

Table 10. Internal and Aeropropulsive Characteristics for Nozzle 7

(a) Internal performance

Table 42. Schedule of Nozzle Pressure Ratio With Mach Number

Table 3. Pressure Orifice Locations on External Surface of Nozzles

(a) Nozzles 1 and 6

Nozzle 1

x/L_n	Upper ramp surface at values of $z/(W/2)$ of— 0	0.34	0.68	0.78
0.053	x	x		
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x		
.526	x			
.579	x	x	x	
.632	x		x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842	x		x	
.863				x
.895	x	x		
.914				x
.947	x		x	

Nozzle 6

x/L_n	Upper ramp surface at values of $z/(W/2)$ of— 0	0.34	0.68	0.78
0.053	x			
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x		
.526	x		x	
.579			x	
.632			x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842	x		x	
.863				x
.895	x	x	x	
.914				x
.947		x	x	

Nozzle 1

x/L_n	Lower flap surface at values of $z/(W/2)$ of— 0	0.68
0.063	x	x
.126	x	x
.189	x	x
.252	x	x
.315	x	x
.378	x	x
.442		x
.505	x	x
.568	x	x
.631	x	x

Nozzle 6

x/L_n	Lower flap surface at values of $z/(W/2)$ of— 0	0.68
0.063	x	x
.118	x	x
.178	x	x
.237	x	x
.296	x	x
.355	x	x
.415		x
.475	x	x
.533	x	x
.592	x	x

Table 3. Continued

(b) Nozzles 8 and 10

Nozzle 8

x/L_n	Upper ramp surface at values of $z/(W/2)$ of—			
	0	0.34	0.68	0.78
0.053	x	x		
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x		
.526	x		x	
.579		x	x	
.632	x		x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842			x	
.863				x
.895	x	x		
.914				x
.947		x	x	

x/L_n	Lower flap surface at values of $z/(W/2)$ of—	
	0	0.68
0.063	x	x
.126	x	x
.189	x	x
.252	x	x
.315	x	x
.378	x	x
.442		x
.505	x	x
.568	x	x
.631	x	x

Nozzle 10

x/L_n	Upper ramp surface at values of $z/(W/2)$ of—			
	0	0.34	0.68	0.78
0.053	x	x		
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x	x	
.526	x		x	
.579		x	x	
.632	x		x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842	x		x	
.863				x
.895	x	x	x	
.914				x
.947	x	x	x	

x/L_n	Lower flap surface at values of $z/(W/2)$ of—	
	0	0.68
0.063	x	x
.118	x	x
.178	x	x
.237	x	x
.296	x	x
.355	x	x
.415		x
.475	x	x
.533	x	x
.592	x	x

Table 3. Concluded

(c) Nozzles 33 and 36

Nozzle 33

x/L_n	Upper ramp surface at values of $z/(W/2)$ of— 0	0.34	0.68	0.78
0.053	x	x		
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x	x	
.526	x		x	
.579	x	x	x	
.632	x		x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842	x		x	
.863				x
.895	x	x		
.914				x
.947	x	x	x	

x/L_n	Lower flap surface at values of $z/(W/2)$ of— 0	0.68
0.063	x	x
.126	x	x
.189	x	x
.252	x	x
.315	x	x
.378	x	x
.442		x
.505	x	x
.568	x	x
.631	x	x

Nozzle 36

x/L_n	Upper ramp surface at values of $z/(W/2)$ of— 0	0.34	0.68	0.78
0.053	x	x		
.105			x	
.158	x	x	x	
.211	x		x	
.263	x	x	x	
.316	x		x	
.368	x	x	x	
.421			x	
.474	x	x	x	
.526	x		x	
.579	x	x	x	
.632	x		x	
.684	x	x	x	
.737	x		x	
.760				x
.789	x	x	x	
.811				x
.842	x		x	
.863				x
.895	x	x	x	
.914				x
.947	x	x	x	

x/L_n	Lower flap surface at values of $z/(W/2)$ of— 0	0.68
0.056	x	x
.112	x	x
.168	x	x
.224	x	x
.280	x	x
.337	x	x
.393		x
.449	x	x
.505	x	x
.561	x	x

(a) Nozzle geometry.

Figure 1. Nozzle and sidewall geometries. All linear dimensions are given in inches.

(b) Sidewall geometry.

Figure 1. Concluded.

Figure 2. Propulsion simulation system with typical nozzle configuration installed. All linear dimensions are given in inches.

Figure 3. Schematic cross section of high-pressure airflow transfer system.

L-92-40

Figure 4. Model with nozzle 29 installed in the Langley 16-Foot Transonic Tunnel.

(a) $M = 0; \alpha = 0°$.

Figure 5. Effect of upper ramp length on aeropropulsive performance. $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12$.

(b) $M = 0.60; \alpha = -6°$.

Figure 5. Continued.

(c) $M = 0.60; \alpha = 0°$.

Figure 5. Continued.

(d) $M = 0.60; \alpha = 6°$.

Figure 5. Continued.

(e) $M = 0.80; \alpha = 0°$.

Figure 5. Continued.

(f) $M = 0.90; \alpha = -6°$.

Figure 5. Continued.

(g) $M = 0.90; \alpha = 0°$.

Figure 5. Continued.

(h) $M = 0.90; \alpha = 6°$.

Figure 5. Continued.

(i) $M = 0.95; \alpha = 0°$.

Figure 5. Continued.

(j) $M = 1.20; \alpha = 0°$.

Figure 5. Concluded.

Figure 6. Effect of upper ramp length on aeropropulsive performance at scheduled NPR. $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 7. Effect of upper ramp length on nozzle drag. $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 8. Effect of upper ramp length on aeropropulsive performance at angle of attack at scheduled NPR. $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12$.

(a) $M = 0.60$.

Figure 9. Effect of upper ramp length on nozzle drag at angle of attack. $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12$.

(b) $M = 0.90$.

Figure 9. Concluded.

(a) $M = 0$.

Figure 10. Effect of upper ramp angle on aeropropulsive performance. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_t = 7.25; y_t/h_t = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 10. Continued.

(c) $M = 0.80$.

Figure 10. Continued.

(d) $M = 0.90$.

Figure 10. Continued.

(e) $M = 0.95$.

Figure 10. Continued.

(f) $M = 1.20$.

Figure 10. Concluded.

Figure 11. Effect of upper ramp angle on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 12. Effect of upper ramp angle on nozzle drag. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 13. Effect of lower flap length on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; \theta_f = 0°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 13. Continued.

(c) $M = 0.80$.

Figure 13. Continued.

(d) $M = 0.90$.

Figure 13. Continued.

(e) $M = 0.95$.

Figure 13. Continued.

(f) $M = 1.20$.

Figure 13. Concluded.

Figure 14. Effect of lower flap length on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50$; $\theta_r = 8.75°; \beta_r = 8.15°; \theta_f = 0°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 15. Effect of lower flap length on nozzle drag. $l_f/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; \theta_f = 0°$; $x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 16. Effect of lower flap angle on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°$; $\beta_r = 8.15°; l_f/h_{t,n} = 0.90; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 16. Continued.

(c) $M = 0.80$.

Figure 16. Continued.

(d) $M = 0.90$.

Figure 16. Continued.

(e) $M = 0.95$.

Figure 16. Continued.

(f) $M = 1.20$.

Figure 16. Concluded.

Figure 17. Effect of lower flap angle on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50$; $\theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 18. Effect of lower flap angle on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°$; $l_f/h_{t,n} = 0.90; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0; \alpha = 0°$.

Figure 19. Effect of axial location of nozzle throat on aeropropulsive performance. $l_r/h_{t,n} = 4.50$; $\theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; y_t/h_{t,n} = 0.12; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69$.

(b) $M = 0.60; \alpha = -6°$.

Figure 19. Continued.

(c) $M = 0.60; \alpha = 0°$.

Figure 19. Continued.

(d) $M = 0.60; \alpha = 6°$.

Figure 19. Continued.

(e) $M = 0.80; \alpha = 0°$.

Figure 19. Continued.

(f) $M = 0.90; \alpha = -6°$.

Figure 19. Continued.

(g) $M = 0.90; \alpha = 0°$.

Figure 18. Continued.

(h) $M = 0.90; \alpha = 6°$.

Figure 19. Continued.

(i) $M = 0.95; \alpha = 0°$.

Figure 19. Continued.

(j) $M = 1.20; \alpha = 0°$.

Figure 19. Concluded.

Figure 20. Effect of axial location of nozzle throat on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; y_t/h_{t,n} = 0.12; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69; \alpha = 0°$.

Figure 21. Effect of axial location of nozzle throat on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; y_t/h_{t,n} = 0.12; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69; \alpha = 0°$.

Figure 22. Effect of axial location of nozzle throat on aeropropulsive performance at angle of attack at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; y_t/h_{t,n} = 0.12; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69$.

(a) $M = 0.60$.

Figure 23. Effect of axial location of nozzle throat on nozzle drag at angle of attack. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; y_t/h_{t,n} = 0.12; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69$.

(b) $M = 0.90$.

Figure 23. Concluded.

(a) $M = 0; \alpha = 0°$.

Figure 24. Effect of vertical location of nozzle throat on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; l_f/h_{t,n} = 0.90; \theta_f = 0°; x_t/h_{t,n} = 7.25; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69$.

(b) $M = 0.60; \alpha = -6°$.

Figure 24. Continued.

(c) $M = 0.60; \alpha = 0°$.

Figure 24. Continued.

(d) $M = 0.60; \alpha = 6^\circ$.

Figure 24. Continued.

(e) $M = 0.80; \alpha = 0^\circ$.

Figure 24. Continued.

(f) $M = 0.90; \alpha = -6^\circ$.

Figure 24. Continued.

(g) $M = 0.90; \alpha = 0^\circ$.

Figure 24. Continued.

(h) $M = 0.90; \alpha = 6^\circ$.

Figure 24. Continued.

(i) $M = 0.95; \alpha = 0^\circ$.

Figure 24. Continued.

(j) $M = 1.20; \alpha = 0^\circ$.

Figure 24. Concluded.

Figure 25. Effect of vertical location of nozzle throat on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75^\circ; l_f/h_{t,n} = 0.90; \theta_f = 0^\circ; x_t/h_{t,n} = 7.25; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69; \alpha = 0^\circ$.

Figure 26. Effect of vertical location of nozzle throat on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75^\circ$; $l_f/h_{t,n} = 0.90; \theta_f = 0^\circ; x_t/h_{t,n} = 7.25; (A_e/A_t)_i = 1.19; (A_e/A_t)_e = 1.69; \alpha = 0^\circ$.

(a) $M = 0$.

Figure 27. Effect of varying upper ramp expansion shape on aeropropulsive performance. $l_r/h_{t,n} = 4.50$; $\theta_r = 8.75^\circ; \beta_r = 8.15^\circ; l_f/h_{t,n} = 0.90; \theta_f = 0^\circ; \beta_f = 11.28^\circ; x_t/h_{t,n} = 7.25$; $y_t/h_{t,n} = 0.12; \alpha = 0^\circ$.

(b) $M = 0.60$.

Figure 27. Continued.

(c) $M = 0.80$.

Figure 27. Continued.

(d) $M = 0.90$.

Figure 27. Continued.

(e) $M = 0.95$.

Figure 27. Continued.

(f) $M = 1.20$.

Figure 27. Concluded.

Figure 28. Effect of varying upper ramp expansion shape on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 29. Effect of varying upper ramp expansion shape on nozzle drag. $l_f/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 30. Effect of varying upper ramp external shape on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 30. Continued.

(c) $M = 0.80$.

Figure 30. Continued.

(d) $M = 0.90$.

Figure 30. Continued.

(e) $M = 0.95$.

Figure 30. Continued.

(f) $M = 1.20$.

Figure 30. Concluded.

Figure 31. Effect of varying upper ramp external shape on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 32. Effect of varying upper ramp external shape on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 33. Effect of varying lower flap external shape on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 33. Continued.

(c) $M = 0.80$.

Figure 33. Continued.

(d) $M = 0.90$.

Figure 33. Continued.

(e) $M = 0.95$.

Figure 33. Continued.

(f) $M = 1.20$.

Figure 33. Concluded.

Figure 34. Effect of varying lower flap external shape on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 35. Effect of varying lower flap external shape on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0.90; \theta_f = 0°; \beta_f = 11.28°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 36. Effect of upper ramp length of convergent nozzles on aeropropulsive performance. $\theta_r = 8.75°; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 36. Continued.

(c) $M = 0.80$.

Figure 36. Continued.

(d) $M = 0.90$.

Figure 36. Continued.

(e) $M = 0.95$.

Figure 36. Continued.

(f) $M = 1.20$.

Figure 36. Concluded.

Figure 37. Effect of upper ramp length of convergent nozzles on aeropropulsive performance at scheduled NPR. $\theta_r = 8.75°; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 38. Effect of upper ramp length of convergent nozzles on nozzle drag. $\theta_r = 8.75°; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 39. Effect of upper ramp angle of convergent nozzles on aeropropulsive performance. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 39. Continued.

(c) $M = 0.80$.

Figure 39. Continued.

(d) $M = 0.90$.

Figure 39. Continued.

(e) $M = 0.95$.

Figure 39. Continued.

(f) $M = 1.20$.

Figure 39. Concluded.

Figure 40. Effect of upper ramp angle of convergent nozzles on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 41. Effect of upper ramp angle of convergent nozzles on nozzle drag. $l_r/h_{t,n} = 4.50; l_f/h_{t,n} = 0; \theta_f = -4°; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 42. Effect of lower flap length of convergent nozzles on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 42. Continued.

(c) $M = 0.80$.

Figure 42. Continued.

(d) $M = 0.90$.

Figure 42. Continued.

(e) $M = 0.95$.

Figure 42. Continued.

(f) $M = 1.20$.

Figure 42. Concluded.

Figure 43. Effect of lower flap length of convergent nozzles on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 44. Effect of lower flap length of convergent nozzles on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 45. Effect of lower flap angle of convergent nozzles on aeropropulsive performance. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 45. Continued.

(c) $M = 0.80$.

Figure 45. Continued.

(d) $M = 0.90$.

Figure 45. Continued.

(e) $M = 0.95$.

Figure 45. Continued.

(f) $M = 1.20$.

Figure 45. Concluded.

Figure 46. Effect of lower flap angle of convergent nozzles on aeropropulsive performance at scheduled NPR. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 47. Effect of lower flap angle of convergent nozzles on nozzle drag. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; l_f/h_{t,n} = 0; \beta_f = 12.63°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0$.

Figure 48. Comparison of aeropropulsive performance between convergent-divergent and convergent nozzles. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(b) $M = 0.60$.

Figure 48. Continued.

(c) $M = 0.80$.

Figure 48. Continued.

(d) $M = 0.90$.

Figure 48. Continued.

(e) $M = 0.95$.

Figure 48. Continued.

(f) $M = 1.20$.

Figure 48. Concluded.

Figure 49. Comparison of aeropropulsive performance between convergent-divergent and convergent nozzles. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

Figure 50. Comparison of nozzle drag between convergent-divergent and convergent nozzles. $l_r/h_{t,n} = 4.50; \theta_r = 8.75°; \beta_r = 8.15°; x_t/h_{t,n} = 7.25; y_t/h_{t,n} = 0.12; \alpha = 0°$.

(a) $M = 0.60$.

Figure 51. External pressure distributions for nozzle 1 at $\alpha = 0°$.

(b) $M = 0.80$.

Figure 51. Continued.

(c) $M = 0.90$.

Figure 51. Continued.

(d) $M = 0.95$.

Figure 51. Concluded.

(a) $M = 0.60$; NPR = 1.

Figure 52. Comparison of external pressure distributions for nozzles 1, 6, and 8 at $\alpha = 0°$.

(b) $M = 0.60$; NPR = 3.

Figure 52. Continued.

(c) $M = 0.60$; NPR = 5.

Figure 52. Continued.

(d) $M = 0.80$; NPR = 1.

Figure 52. Continued.

(e) $M = 0.80$; NPR = 3.

Figure 52. Continued.

(f) $M = 0.80$; NPR = 5.

Figure 52. Continued.

(g) $M = 0.90$; NPR = 1.

Figure 52. Continued.

(h) $M = 0.90$; NPR = 3.

Figure 52. Continued.

(i) $M = 0.90$; NPR = 5.

Figure 52. Continued.

(j) $M = 0.90$; NPR = 7.

Figure 52. Continued.

(k) $M = 0.95$; NPR = 1.

Figure 52. Continued.

(l) $M = 0.95$; NPR = 7.

Figure 52. Continued.

(m) $M = 0.95$; NPR = 9.

Figure 52. Concluded.

(a) $M = 0.60$; NPR = 1.

Figure 53. Comparison of external pressure distributions for nozzles 1 and 10 at $\alpha = 0°$.

(b) $M = 0.60$; NPR = 3.

Figure 53. Continued.

(c) $M = 0.60$; NPR = 5.

Figure 53. Continued.

(d) $M = 0.80$; NPR = 1.

Figure 53. Continued.

(e) $M = 0.80$; NPR = 3.

Figure 53. Continued.

(f) $M = 0.80$; NPR = 5.

Figure 53. Continued.

(g) $M = 0.90$; NPR = 1.

Figure 53. Continued.

(h) $M = 0.90$; NPR = 3.

Figure 53. Continued.

(i) $M = 0.90$; NPR = 5.

Figure 53. Continued.

(j) $M = 0.90$; NPR = 7.

Figure 53. Continued.

(k) $M = 0.95$; NPR = 1.

Figure 53. Continued.

(l) $M = 0.95$; NPR = 7.

Figure 53. Continued.

(m) $M = 0.95$; NPR = 9.

Figure 53. Concluded.

(a) $M = 0.60$; NPR = 1.

Figure 54. Comparison of external pressure distributions for nozzles 1, 33, and 36 at $\alpha = 0°$.

(b) $M = 0.60$; NPR = 3.

Figure 54. Continued.

(c) $M = 0.60$; NPR = 5.

Figure 54. Continued.

(d) $M = 0.80$; NPR = 1.

Figure 54. Continued.

(e) $M = 0.80$; NPR = 3.

Figure 54. Continued.

(f) $M = 0.80$; NPR = 5.

Figure 54. Continued.

(g) $M = 0.90$; NPR = 1.

Figure 54. Continued.

(h) $M = 0.90$; NPR = 3.

Figure 54. Continued.

(i) $M = 0.90$; NPR = 5.

Figure 54. Continued.

(j) $M = 0.90$; NPR = 7.

Figure 54. Continued.

(k) $M = 0.95$; NPR = 1.

Figure 54. Continued.

(l) $M = 0.95$; NPR = 7.

Figure 54. Continued.

(m) $M = 0.95$; NPR = 9.

Figure 54. Concluded.